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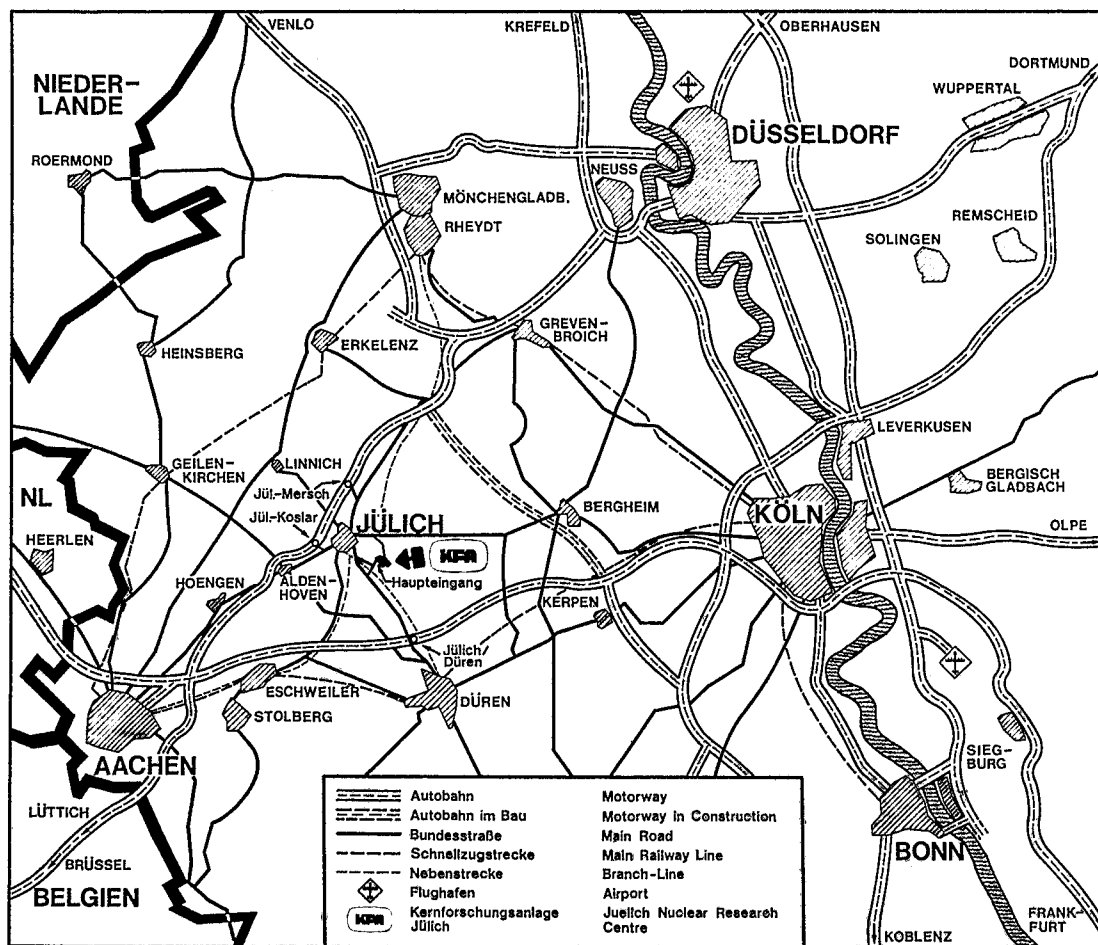
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**Problems of Heat Removal
at First Wall Components of INTOR**

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Problems of Heat Removal at First Wall Components of INTOR

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P. Mioduszewski

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1. INTRODUCTION

The heat loads at the first wall components of future large tokamaks will be greatly different from those of existing devices. The following major differences between the heat loads in present day machines and future tokamaks of INTOR-size /1/ must be taken into consideration: While the average heat load in present tokamaks is between 0.01 and 0.1 MW/m^2 it will be in the range of 0.4 MW/m^2 in the future generation. The pulse duration is at present in the order of 1 s and corresponds to the thermal penetration time in the applied wall materials, thus the total energy can be taken by the thermal inertia of the wall and be removed between discharges. For INTOR, however, the pulse duration is assumed to be 100 s requiring steady state cooling. Finally, the high neutron fluxes in reactor-like devices represent an additional heat source by which the bulk of the first wall is heated. This nuclear heating amounts to about 10 MW/m^3 per 1 MW/m^2 of incident neutron flux in stainless steel.

Except of the normal operating conditions there are fault conditions which cause extreme heat loads on the first wall components like runaway electron discharges or major plasma disruptions. The latter will be discussed here, because they are assumed to represent another problem to the first wall components different from that of present day machines. In these considerations the total thermal energy content of the plasma, related to the plasma surface, is the relevant parameter. In present day tokamaks the energy content is between 20 and 100 kJ whereas the thermal plasma energy of INTOR is assumed to be 200 MJ . Related to the corresponding plasma surface, PLT-like devices yield about $5 \cdot 10^{-3} \text{ MJ/m}^2$ whereas INTOR yields about 0.7 MJ/m^2 . Although the disruption time will probably increase with larger plasma size, the resulting heat loads still are likely to exceed the ones experienced in present experiments by an order of magnitude.

2. FIRST WALL UNDER NORMAL OPERATION

The heat fluxes from the plasma hitting the first wall cause temperature gradients and corresponding thermal stresses in the wall. Due to the pulsed operation the thermal stresses are cyclical with up to 10^6 thermal cycles. For a 316 stainless steel wall the maximum cyclical stress should not exceed 200 MPa. This restricts the first wall thickness to about 4 mm for the fluxes anticipated for INTOR, i.e. 0.4 MW/m^2 thermal flux and about 16 MW/m^3 nuclear heating in the bulk. On the other hand, the vacuum vessel wall requires several cm of wall thickness, and the thermal stresses would exceed the yield stress several times if the wall was hit by the full heat fluxes from the plasma. Therefore, the first wall must be made of structures with sufficient small radii to allow thinner walls. This can either be done with cooling panels acting as the first wall and removing the heat, or with complete blanket cells removing the heat and simultaneously breeding the fuel.

2.1 Cooling Panels

With cooling panels made of folded sheet metal or welded-together tubes sufficient high heat loads can be handled. This is illustrated by the following example: a panel of welded-together stainless steel tubes with 4 cm diameter and 2 MPa coolant pressure is irradiated with total power fluxes from the plasma which correspond to 0.5; 1.0; 1.5; 2.0; 2.5 and 3 MW/m^2 respectively. The resulting total stresses, including hoop stress and thermal stress, as function of wall thickness are plotted in fig. 1 according to

$$\sigma = \frac{p \cdot d}{2 \Delta l} + \frac{1}{2} \frac{\alpha E}{(1-\nu)} \frac{1}{K} \left(\dot{q}_s \Delta l + \frac{1}{2} \dot{q}_v \Delta l^2 \right) \quad (1)$$

with p , d , Δl being the coolant pressure, pipe diameter and wall thickness respectively and α , E , ν , K the coefficient of thermal expansion, Young's modulus, Poisson's ratio and thermal conductivity respectively. The total heat absorbed by the wall is given by the surface heat load q_s and the nuclear bulk heating due to neutrons q_N . The surface heat load q_s is 20 % of the total wall load and the nuclear heating rate q_N is assumed to be 10 MW/m^3 per 1 MW/m^2 of incident neutron power.

For the maximum tolerable stress we choose a safety factor of 1.5 with respect to the 0.2 % yield stress. For our reference case of 0.4 MW/m^2 surface load plus 16 MW/m^3 bulk heating the plot yields a maximum wall thickness of 6.5 mm. The maximum cyclical stress as given by the fatigue limit is 200 MPa for stainless steel which restricts the wall thickness to 4.2 mm. Since the wall erosion due to particle bombardment might be as high as 1 mm/year, this type of cooling panel would have to be replaced after about two or three years. To guarantee a sufficient safety margin with respect to coolant spillage into the vacuum vessel, the minimum wall thickness should definitely be greater than the minimum required by stress analysis only. The severity of a coolant accident also depends on the choice of coolant, like e.g. water or helium. At any rate, the concept of cooling panels with the corresponding system of cooling ducts inside the vacuum vessel will certainly cause additional remote maintenance and repair problems.

As an alternative the potential of aluminium as a first wall material is considered. Due to the better thermal conductivity the thermal gradients and the corresponding stresses are lower than in stainless steel. A major drawback of aluminium is its reduced strength at elevated temperatures. The wall temperatures will probably have to be kept below 200°C which makes water-cooling appropriate. Due to the low wall temperatures aluminium has probably no potential for reactor applications but seems attractive for intermediate devices. To evaluate the thermal properties of an aluminium first wall we compute the stresses in an aluminium tube of 4 cm inner diameter with water cooling at $T = 200^\circ\text{C}$ and a pressure of 1.5 MPa. Figure 2 shows the total stress at various heat loads as function of wall thickness. The nuclear bulk heating is assumed to be 6 MW/m^3 per 1 MW/m^2 of incident neutron flux. At 0.4 MW/m^2 surface heat load plus 9.6 MW/m^3 bulk heating and with a safety factor of 1.5 with respect to the 0.2 % yield stress a maximum wall thickness of 2.5 cm is allowed. For aluminium the maximum allowable stress is not restricted by thermal fatigue. Considering various aluminium alloys, even the most unfavourable case of SAP (Sintered Aluminium Powder) with 4 % Al_2O_3 at $T = 400^\circ\text{C}$

and $n = 10^6$ cycles yields a maximum tolerable cyclic stress of about 70 MPa, i.e. slightly above $\sigma_{0.2}/1.5$.

These results suggest that first wall parts which are not covered with blanket cells might be designed as massive aluminium walls that are water-cooled by internal ducts. Even at erosion rates of 1 mm/year the walls would not have to be replaced during the lifetime of the experiment, unless required by other reasons. The advantages of an aluminium first wall include: good heat accommodation, low tritium permeation, low induced activity, low-Z wall material and high tolerable erosion rates.

2.2. Blanket-Cell Wall

In pre-demonstration fusion devices like INTOR parts of the first wall will be covered with blanket cells as used in later reactors for tritium breeding and heat generation. In INTOR these blanket-cells are presumed to be experimental units and will probably be exchangeable rather than permanent. As an example we consider helium-cooled, cylindrical cells of 316 CW stainless steels pointing radially into the plasma chamber /2/. One of these blanket-cells is shown schematically in fig. 3. It is essentially 0.5 m in diameter and 0.5 m in length. The hoop stress as well as thermal stress and total stress in the cell wall exposed to the plasma is plotted in fig. 4 for coolant pressures of 1.0; 1.5, and 2.0 MPa, respectively. The maximum allowable wall thickness due to the thermal stress limit is 5.4 mm for a medium coolant pressure of 1.5 MPa. The fatigue limit reduces the wall thickness to 4.2 mm if it is to be designed for the full lifetime of 10^6 discharges. Since the blanket cells, however, are part of the test program and likely to be exchanged frequently, the fatigue limit at 10^4 cycles might be chosen for the design which yields a wall thickness of 5.2 mm. This is a pessimistic estimate because it is based on a rather high total wall load of 2 MW/m² and the assumption that the thermal power is deposited on the first wall proper rather than being shared by other components like limiter or divertor target. For a more realistic case which yields 0.3 MW/m² surface heat and 12 MW/m³ bulk heating the allowable wall thickness will be approximately 6 mm.

In addition to the steady state conditions the heating-and cooling-phase of the blanket cell first wall shall be considered. As can be seen from fig. 3 the first wall of the blanket cell is cooled preferentially as compared to the bulk of the blanket. This yields a highly efficient first wall cooling on the one hand but a high thermal cycling on the other hand. For the special helium flow scheme employed here the heat transfer coefficient between the coolant and the first wall is about $2 \cdot 10^3 \text{ W/m}^2\text{K}$. With this value and the thermal properties of 316 stainless steel the temperature rise of a 5 mm cell wall has been computed assuming a step function for the heat flux. Since stainless steel has a poor thermal conductivity the front and rear wall temperatures are quite different and a lumped thermal capacity approach cannot be applied. For the case of constant power flux to the front surface and given heat transfer at the rear surface numerical solutions for the temperature rise can be taken from the literature /3/. The results as applied to our case are shown in fig. 5. Starting at the coolant temperature of 220°C the front wall temperature rises to 475°C and the rear wall temperature rises to 390°C . The maximum temperatures are reached after 70 seconds.

For practical reasons it is convenient to have approximate analytical solutions which can describe the temperature-rise as well as the decay. For this reason we have modified the lumped thermal capacity approach by introducing a mean wall temperature plus a temperature gradient as a function of the mean temperature. With this approach the heat transfer to the coolant can be handled more correctly because for finite thermal conducting the wall surface in contact with the coolant is always at a temperature below the mean wall temperature as given by the corresponding thermal gradient. The result of this procedure is also plotted in fig. 5 and is evidently a quite satisfactory approximation. The plot reveals that the first wall temperature drops to almost the coolant temperature due to the effective cooling scheme at the first wall. Thus, the first wall is subject to a full thermal cycle for each plasma pulse, presumed that the coolant flow is the same during pulse time and dwell time. If, however, the coolant flow could be controlled in a way to reduce it

during dwell time, the thermal cycling and, correspondingly, the thermal fatigue problems could be reduced. The remaining question is whether or not the control of large coolant flow with valves for the corresponding number of cycles is technically feasible.

3. HEAT REMOVAL VIA A TOROIDAL LIMITER

Although some form of magnetic divertor is probably required in INTOR to provide for helium exhaust, the potential of material limiters is worthwhile to be discussed by several reasons. If non-divertor solutions for ash removal could be found, limiters seem to be most attractive for heat removal and wall protection because of their simplicity as compared to magnetic divertors. In addition, one or the other type of a wall protection device might be necessary any way to handle the excessive heat loads during disruptions, to absorb the neutral beam shine-through or simply to act as a back-up for the divertor. In INTOR the fusion power during burn is expected to be in the range of 600 MW with a corresponding thermal wall load of about 120 MW. Presumed that the limiter has to be designed to take up to 80 % of the total thermal plasma power, it has to accommodate about 100 MW of thermal power. This power cannot be removed with poloidal limiters like in present day machines, and methods have to be found to spread the heat over a surface area as large as possible. For this purpose the potential of toroidal limiters has been studied with respect to an application in TFTR [4] and other tokamaks of the next generation. In the present paper we study the applicability of this concept to INTOR with special emphasis on steady state operation, on the heat load distribution due to non-circular plasma cross-sections, and on impurity generation.

3.1 Plasma-Limiter Interaction Model

The calculations of the heat flux distribution on the limiter are based on the following assumptions: The plasma energy flows - via heat conduction and convection - radially out of the plasma core into the limiter scrape-off layer. Within the scrape-off layer the energy flows along the magnetic field lines to the limiter surface. The e-folding length δ of the scrape-off layer for typical INTOR plasmas has been studied in [5] to yield between about 3 cm and 5 cm. For our purpose we choose a pessimistic value of $\delta = 3$ cm.

For steady state heat removal the energy flux density should not exceed $5 - 7 \text{ MW/m}^2$. To remove 100 MW in INTOR, a limiter surface of $15 - 20 \text{ m}^2$ is required. This can only be achieved if the power flux in the scrape-off layer hits the limiter surface - in the poloidal projection - by oblique incidence, as illustrated in fig. 6.

Fig. 6 shows a poloidal projection of the plasma with minor radius a and two extreme cases of limiter configurations. In the most simple case the poloidal projection of the limiter is a straight tangential line and in the other case the limiter surface is curved with radius R_L and wrapped around half of the plasma column. We study the surface heat load for these two extreme examples and some other cases in between. To avoid excessive heat loads to the first wall at the limiter edge the limiter blade is extended over 3 e-folding lengths of the scrape-off layer to absorb 95 % of the total power. The location of the limiter is preferably at the inboard side of the torus. This way several tasks can be accomplished by the limiter: normal limiter action, protection of the first wall from hard disruptions [6], absorption of the injected neutral beam shine-through.

3.2 Heat Load Distribution on the Limiter

Under the assumption that the energy flux density $\dot{q}(\varrho)$ in the scrape-off layer falls off exponentially with the distance into the scrape-off layer, we get

$$\dot{q}(\varrho) = \dot{q}_0 \exp\left(-\frac{\varrho-a}{\delta}\right) \quad (1)$$

where ϱ is the polar radius of the limiter and $\dot{q}_0$ is the maximum power density at $\varrho = a$. The corresponding poloidal projections are:

a) for the straight tangential limiter:

$$\dot{q}(\gamma) = \dot{q}(\varrho) \cdot (d\varrho/d\gamma) \quad (2)$$

b) for the curved tangential limiter:

$$\dot{q}(S) = q(\varphi) \cdot (d\varphi/dS) \quad (3)$$

The arc length on the curved limiter given by $S = R_L \cdot \alpha$ is referred to as perimeter S .

We insert $\varphi = \sqrt{Y^2 + a^2}$ for the straight limiter and $\varphi = \sqrt{R_L^2 + \varphi_0^2 - 2R_L\varphi_0 \cos(S/R_L)}$ with $\varphi_0 = R_L - a$ for the curved limiter into eqs. (2) and (3) respectively. Integration of both equations over the poloidal and toroidal directions yields the scale factor q_0 and consequently the energy flux distribution as a function of the limiter perimeter S or Y respectively:

$$\dot{q}(Y) = \frac{P}{4\pi(R_0 - a) \cdot 0.95\delta} \frac{Y}{\sqrt{Y^2 + a^2}} \cdot \exp\left(-\frac{\sqrt{Y^2 + a^2} - a}{\delta}\right) \quad (4)$$

$$\dot{q}(S) = \frac{P}{4\pi(R_0 - a) \cdot 0.95\delta} \frac{\varphi_0 \sin(S/R_L)}{\sqrt{R_L^2 + \varphi_0^2 - 2R_L\varphi_0 \cos(S/R_L)}} \cdot \exp\left(-\frac{\sqrt{R_L^2 + \varphi_0^2 - 2R_L\varphi_0 \cos(S/R_L)} - a}{\delta}\right) \quad (5)$$

where P is the thermal power to the limiter and the factor 0.95 results from integration over 3 e-folding lengths δ . For the assumed INTOR parameters $P = 100$ MW, $a = 1.64$ m and $\delta = 3$ cm we get the energy flux distribution as a function of the perimeter S or Y respectively as shown in fig. 7. Since the energy deposition is symmetrical to $S = 0$ ($Y = 0$) we have plotted only one half of the power deposition profiles. The profiles no. 1 through 4 correspond to the curved tangential limiter with various limiter radii R_L and profile no. 5 corresponds to the straight tangential limiter.

Fig. 7 gives estimates for the heat load on the limiter when the plasma cross-section is circular. Actually the plasma cross-section in INTOR will be non-circular and - as a consequence - the contact area between plasma and limiter will get larger with increasing non-circularity to help spread out the power flux. Although the shape of the INTOR plasma is presumably not exactly elliptical, we demonstrate the effect of noncircularity by applying an ellipse with minor axis b and major axis a and $a/b = 1.6$.

For simplicity the ellipse can be approximated by the corresponding osculating circle with radius a^2/b yielding a deviation from the ellipse smaller than 6 % for $\psi \leq 30^\circ$ (cf. fig. 6). This approach cannot be applied for the case indicated by curve no. 1 in fig. 7 where $\psi = 90^\circ$.

For INTOR the minor axis of the elliptical plasma cross-section is assumed to be $b = 1.3$ m and the elongation $\mathcal{E} = 1.6$ which gives $a^2/b = 3.328$ m. Replacing $(R_0 - a)$ in eqs. (4) and (5) by $(R_0 - b)$ and a by a^2/b elsewhere we get some estimates for the limiter heat load due to elliptical plasma cross-sections. The result is plotted in fig. 8. As can be seen the maximum energy flux density is lowered by about a factor of 1.6 as compared to the circular case. Since the actual cross-section of the INTOR plasma is probably more D-shaped rather than elliptical with the flat side of the D in contact with the limiter, the non-circularity effect on the heat load is likely to be more pronounced than demonstrated here.

3.3 Thermal Response of the Limiter

For a proper calculation of the thermal response of the limiter we need to employ a two-dimensional heat conduction formalism corresponding to the heat flux distribution given by figs. 7 and 8. At this stage and for first estimates, however, the following procedure seems to be good enough: We assume that the whole limiter is made of graphite tiles - as proposed for TFTR - 10×10 cm² and 2 cm thick. It is further assumed that each graphite tile receives a mean energy flux density P_0 and that heat conduction between different tiles is negligible. As two special examples we consider the cases represented by curves no. 4 and 5 in fig. 8. The tiles are numbered 1 through 11 along the perimeter as shown in fig. 8. For our computation we pick the corresponding tiles with the highest heat loads. For reasons of maintenance and repair the graphite tiles are preferably cooled by radiation cooling, whereas some kind of backing plates have probably to be cooled actively in order to avoid intolerable heat loads on the first wall.

First we check on the temperature oscillations due to the pulsed heat load with 100 s pulse time and 40 s dwell time. An isolated graphite tile of the dimensions given above is irradiated with the energy flux density P_o . With rising temperature it also loses energy by radiation to the surrounding medium at T_o , according to $P_R = 2 \epsilon \sigma (T^4 - T_o^4)$, where the emissivity for graphite is taken to be $\epsilon = 0.8$, the Stefan-Boltzmann constant $\sigma = 5.67 \cdot 10^{-8} \text{ W/m}^2 \text{K}^4$ and the factor 2 is due to radiation from both sides. For the lumped thermal capacity approach the heating up of a tile with mass m , specific heat c and surface area A is given by

$$mc \frac{dT}{dt} = A \{ P_o - 2 \epsilon \sigma (T^4 - T_o^4) \}. \quad (6)$$

Integrating this differential equation yields the heating time t_H as a function of temperature T . Substituting $(P_o / 2 \epsilon \sigma + T_o^4) = x$ we get:

$$t_H = \frac{mc}{2A\epsilon\sigma} \left\{ \frac{1}{4x^{3/4}} \ln \frac{x^{1/4} + T}{x^{1/4} - T} + \frac{1}{2x^{3/4}} \operatorname{arctg} \frac{T}{x^{1/4}} \right\} + C \quad (7)$$

where C has to be determined from the initial condition $T = T_i$ at $t = 0$. Assuming that the initial limiter temperature is equal to the first wall temperature we choose $T_i = T_o = 800 \text{ K}$.

The cooling down between burn times can be calculated by means of eq. (6) with $P_o = 0$. If cooling down by radiation begins at $t = 0$ with initial temperature T_i , integration of eq. (6) with $P_o = 0$ yields

$$t_{cool} = \frac{mc}{2A\epsilon\sigma} \left\{ \frac{1}{4T_o^3} \ln \frac{T_o + T}{T_o - T} + \frac{1}{2T_o^3} \operatorname{arctg} \frac{T}{T_o} \right\} + C \quad (8)$$

where, again, C has to be determined from $T = T_i$ at $t = 0$. Heating up and cooling down of the graphite tiles for $P_o = 4 \text{ MW/m}^2$ and 2 MW/m^2 is shown in fig. 9. During the first cycle steady state temperature is reached fully at 4 MW/m^2 and up to 95 % at 2 MW/m^2 . Therefore, the steady state temperature is taken to estimate the erosion of the tiles due to evaporation.

For a one-dimensional approach we start with the assumption of simple, separated tiles and the heat load distributions given in fig. 8. By attributing average power densities P_i to the individual tiles - according to fig. 8 - the corresponding steady state temperatures are computed. For a given temperature of the graphite tile the vapour pressure and the corresponding evaporation rate can be calculated [7] for each individual tile. In fig. 10 the mean tile temperatures along with the corresponding evaporation rates are plotted for the limiter configurations 4 and 5. For these plots it can be seen that radiation cooling is not trivial for all limiter configurations considered here. At $T = 2500$ K the vapour pressure of graphite is several mTorr, representing an impurity source for the plasma. To estimate the total amount of generated impurity atoms the evaporation rate of each tile is integrated over the toroidal circumference. Assuming a particle confinement time of $\tau_p = 1$ s and the total number of plasma particles in INTOR of $N = 3 \cdot 10^{22}$ the resulting impurity levels are 27 % for limiter configurations no. 5 and 1 % for limiter no. 4. Whereas 27 % of C-atoms is clearly not acceptable for a burning fusion plasma the impurity level of 1 % - as produced by limiter no. 4 - seems to be tolerable. One has to keep in mind that this is the impurity production by evaporation only, other processes like sputtering and arcing are not considered.

4. SURFACE HEAT LOADS DURING MAJOR DISRUPTIONS

In a major plasma disruption the total thermal energy contained in the plasma is dumped into the first wall in a time which is short compared to the energy confinement time. Thus, the surface heat load on the first wall increases by orders of magnitude for a short time. In INTOR a thermal plasma energy of about 200 MJ is assumed to be deposited in the first wall within about 20 ms. This yields a total power of 10^4 MW as compared to 100 MW for the steady state case of 2 s energy confinement time. In addition, the magnetic field energy contained in the plasma current will be set free during a disruption and parts of it can possibly contribute to the first wall heat load via eddy currents. Whether or not the first wall can handle these extreme heat loads can only be judged when the disruption time as well as the affected surface area are known more exactly. In typical disruptions of present day experiments the width of the negative spike in the loop voltage corresponds roughly to the growth rate of the $m = 2, n = 1$ MHD instability. This growth rate is, therefore, taken to be the best approximation for extrapolating the disruption time. The corresponding scaling laws [8] yield about 20 ms for INTOR. For the affected surface area only guesses can be made up to now. In the present paper a model for the energy deposition mechanism during a disruption is discussed which gives some estimates for the heat loads which have to be expected.

4.1 Plasma Disruption Model

The model for the interaction of the thermal plasma energy with the first wall is based on two major assumptions:

- a) in a "hard disruption" the plasma is dumped on the inboard side of the torus,
- b) the magnetic flux surfaces are conserved and scraped-off successively during the inward movement of the plasma column.

We further assume that the plasma density- and temperature-profiles are parabolic and that the plasma column moves with constant velocity towards the wall. For further discussion of the model we make use of fig. 11. For the first approach a circular plasma cross-section with equivalent radius $a^* = \sqrt{a \cdot b}$ is considered, where a and b are the major and minor axis of the corresponding elliptical cross-section. While the plasma column collapses towards the inboard side of the first wall the minor plasma radius r decreases with time t according to $r = a^*(1-t/\tau_D)$, a being the plasma radius at $t = 0$ and τ_D being the disruption time. When the plasma column with radius $r \leq a^*$ is in contact with the first wall at location P, a scrape-off layer forms, in which the energy is transported along the magnetic field-lines to the first wall and the energy density falls off exponentially with the distance into the scrape-off layer:

$$\varepsilon = \varepsilon_0 \exp\left(-\frac{\varrho-r}{\delta}\right) \quad (9)$$

where δ is the e-folding length in the scrape-off layer as discussed in [5] and the energy content of the scrape-off layer is set equal to the energy contained in the flux tube given by $(\varrho - r)$ of the initial plasma column. With the total plasma energy of

$$E = \frac{3}{2} n k (T_e + T_i), \quad (T_e = T_i) \quad (10)$$

and

$$n(r) = n_0 \left(1 - r^2/a^{*2}\right) \quad (11)$$

$$T(r) = T_0 \left(1 - r^2/a^{*2}\right) \quad (12)$$

we get the plasma energy as function of minor radius

$$q_{th}(r) = q_0 \left(1 - r^2/a^{*2}\right)^2 \quad (13)$$

with $q_0 = 3n_0 k T_0$. Integrating eq. (13) over the poloidal and toroidal coordinates yields the total thermal energy of the plasma.

4.2 Energy Deposition on the First Wall

Assuming toroidal symmetry we compute the plasma energy contained in the ring between r and $r + dr$:

$$dq = 3n_0 k T_0 [1 - (r/a^*)^2]^2 \cdot 2\pi R_0 \cdot 2\pi r dr \quad (14)$$

$$dq/dr = 12\pi^2 R_0 n_0 k T_0 [1 - (r/a^*)^2]^2 r. \quad (15)$$

Replacing the variable r by the variable t using $r = a^*(1-t/\tau_D)$ and differentiating with respect to time the total energy deposition per unit time is:

$$\dot{q}(t) = -12\pi^2 R_0 a^{*2} n_0 k T_0 [1 - (1-t/\tau_D)^2]^2 (1-t/\tau_D) \cdot \frac{1}{\tau_D}, \quad (16)$$

To evaluate the power flow between the plasma and the first wall we employ the model which describes the interaction of the plasma column with a toroidal limiter as described in chapter 3. The inboard side of the first wall is designed as a toroidal limiter the elements of which serve as sacrificial elements in case of plasma disruptions. The poloidal projection of the limiter is curved in a way to keep the power deposition during steady state operation on a tolerable power deposition as function of limiter positions $S = R_L \cdot \alpha$ at steady state is given by:

$$\dot{q}(S) = \frac{P}{4\pi(R_0 - a^*)0.95\delta} \frac{\rho_0 \sin(S/R_L)}{S} \exp\left(-\frac{\rho - a}{\delta}\right)$$

with P = total power deposited on the limiter

$$\rho = \sqrt{R_L^2 + \rho_0^2 - 2 R_L \rho_0 \cdot \cos(S/R_L)}$$

R_L = poloidal radius of curved limiter

$$\rho_0 = R_L - a^*$$

$$a^* = \sqrt{a \cdot b} \text{ mean plasma radius at steady state}$$

S = poloidal perimeter of curved limiter.

The scrape-off length is kept constant during the disruption.

For the inward moving plasma column, during a disruption, we replace the constant power P in eq. (5) by the time dependent power flow given by eq. (16) and the constant plasma radius a by the time dependent plasma radius $r = a^* \cdot (1-t/\tau_D)$. This, finally, yields the heat load on the first wall (limiter) as a function of time and space which is plotted in fig. 12. In this figure the energy flux density $Q[\text{MW/m}^2]$ is plotted as a function of the perimeter S and the time $t \leq \tau_D$. The parameters are: circular plasma cross-section with mean radius $a^* = \sqrt{a \cdot b} = 1.64\text{m}$, limiter curvature $R_L = 1.2 \cdot a^*$, e-folding length of scrape-off-layer $\delta = 0.04\text{m}$. The plasma energy has a profile according to eq. (13) with $q_0 = 3 n_0 k T_0 = 1.92\text{ MJ}$, $n_0 = 2 \cdot 10^{20}\text{ m}^{-3}$ and $T_0 = 20\text{ keV}$.

Since, in our model, the energy deposition is symmetrical to the equatorial midplane of the torus, fig. 12 shows only one half of the poloidal projection of the energy flux density profile. The plot reveals that the local maximum of the deposition profile is shifted inwards with increasing disruption time. The maximum load occurs at about $t = (2/3) \cdot \tau_D$ and at approximately $S = 0.2\text{ m}$. Qualitatively this result can be explained by the fact that the energy content per flux tube is a function of energy density and the volume of a particular flux tube. While the energy density of a flux tube increases with decreasing plasma radius r , due to the profile effect, the corresponding volume decreases. At first the increase of the energy density is faster than the decrease of the volume and the energy deposited per unit time is rising. Eventually, for a certain radius r the volume effect outweighs the profile effect and the energy deposition per unit time decreases.

If the first wall (limiter) is considered as an arrangement of discrete graphite tiles of 2 cm thickness and $10 \times 10\text{ cm}^2$ each, it is useful to plot the evolution of the heat load as a function of time for a single tile. This is shown in fig. 13 for the tile with the highest heat load, at $S = 0.2\text{ m}$. The peak heat load, which occurs at 13 ms , is almost $1.1 \cdot 10^3\text{ MW/m}^2$.

This is a rather high load, even for short pulses in the millisecond range. The actual load, however, is supposedly somewhat lower due to the non-circular plasma cross-section which helps to spread the energy over a larger surface area. To see the non-circularity effect on the heat load distribution, we evaluate eq. (5) for an elliptical plasma cross-section with the following approximations: the energy content in an elliptical flux tube is equal to the energy in the equivalent circular flux tube; the scrape-off layer thickness is kept constant during the inward motion of the plasma column. Since the contact area between the plasma and the first wall is confined to the vicinity of the inboard equatorial line of the torus, the ellipse is approximated by the corresponding osculating circle.

For an INTOR plasma with elliptical cross-section of $b = 1,3$ m and $a/b = 1,6$ the radius of the corresponding osculating circle is $a^2/b = 3,328$ m. The radius of the limiter has been chosen to be $R_L = 1,2 (a^2/b)$. With all other parameters as given above the power deposition on the first wall has been evaluated and is plotted in fig. 14. The peak heat load is now shifted slightly to $t = 15$ ms and S between 0.2 and 0.3 m. The maximum energy flux density on the tile with the highest load is about 750 MW/m^2 as shown in fig. 15. This is a reduction by a factor of 1,5 as compared to the circular case. This reduction might be still higher since the actual plasma is likely to be D-shaped and thus provide for an even larger contact area with the limiter.

4.3 Thermal Response of the First Wall

In our concept the inboard side of the first wall is lined with graphite tiles to form the limiter which also serves as plasma energy dump during disruptions. The thermal response of this part of the first wall is being studied. The graphite tile which receives the highest heat load during a disruption is located between $S = 0.2$ and $S = 0.3$ m. At this location the

the radius $R_L = 1,2 \cdot (a^2/b)$. The corresponding steady state temperature of the radiation-cooled tile is 1700 K. With this initial temperature and a heat pulse as plotted in fig. 15, one-dimensional heat conduction calculations yield a surface temperature rise up to the sublimation temperature of 3925 K within approximately 11 ms. Radiation cooling at $T = 3925$ K amounts to about 10 MW/m^2 . Thus, for a proper evaluation, a sophisticated heat conduction formalism including phase changes has to be applied. Instead, we estimate the upper limit for the amount of ablated material by applying conservation of energy.

With H_s being the heat of sublimation, T_s the sublimation temperature and T_0 the initial temperature, ϱ the density, V the volume, c_p the specific heat of the wall material and E_{pl} the plasma energy dumped into the corresponding part of the wall, conservation of energy yields:

$$E_{pl} = \varrho V [c_p (T_s - T_0) + H_s]. \quad (17)$$

With the affected surface area A , the thickness x of the respective ablated layer is

$$x = \frac{E_{pl}}{A \cdot \varrho [c_p (T_s - T_0) + H_s]}. \quad (18)$$

Substituting for the materials properties of graphite:

$$\varrho = 2,2 \cdot 10^3 \text{ kg/m}^3$$

$$c_p = 2 \cdot 10^3 \text{ J/kgK}$$

$$H_s = 6 \cdot 10^7 \text{ J/kg}$$

$$T_s = 3925 \text{ K}$$

an initial temperature of $T_0 = 1700$ K, and a time integrated energy density (cf. fig. 15) $E_{pl} = 7 \text{ MJ/m}^2$ the thickness of the ablated layer according to eq. (18) is $50 \text{ } \mu\text{m}$ for the tile with the highest heat load, and $5 \text{ } \mu\text{m}$ at $S = 1\text{m}$.

If the first wall is made of aluminium and not protected by graphite tiles, the melting temperature at the location of the highest heat load will be reached after about 7 ms (one-dimensional heat-conduction). An estimate of the ablated layer thickness on the basis of energy conservation yields about 160 μm per disruption at the maximum of the heat load distribution. Corresponding computations for stainless steel yield 6.5 ms to reach the melting temperature and an erosion of 92 μm per disruption. For heat loads as high as considered here, all of the relevant materials are melting, the different thermal properties only determine the time after which the melting temperature is reached.

The erosion rates estimated here are certainly too high for a reactor since it has been suggested that a reactor must be designed to stand about 10^3 disruptions throughout its lifetime [1]. One has to keep in mind that the above estimates represent only upper limits as given by energy conservation. The actual process of plasma energy deposition might rather be dominated by plasma effects, i.e. after a certain amount of wall material is ablated it is being ionized by the plasma electrons and forms a dense plasma itself. This dense metal- or carbon-plasma subsequently acts as the target into which the remaining plasma energy is dumped and helps to spread the deposited plasma energy via radiation over a larger surface area of the wall. This concept has been introduced as "virtual limiter" [9].

5. SUMMARY AND CONCLUSIONS

The thermal load to the first wall of predemonstration fusion devices like INTOR is assumed to be in the range of 0.2 - 0.4 MW/m². The thermal stresses due to this heat load restrict the first wall thickness to about 5 mm for stainless steel or about 25 mm for aluminium respectively. The wall erosion due to sputtering, on the other hand, is in the range of 0.1 - 1 mm/year. From these two conditions the useful life of a stainless steel first wall is limited to a few years whereas aluminium has the potential of withstanding about 20 years from the erosion standpoint. The drawback of aluminium is its low strength at elevated temperatures such that it has to be operated below 200 °C which is detrimental for the thermodynamic efficiency of a reactor. Development of special alloys which meet the requirements for a first wall better than present day materials is in progress but no conclusions can be drawn yet. Therefore, for a present day design of the first wall we have to employ present day materials which give us the following choice:

- a) stainless steel wall components which last only a few years and must have the capability of remote maintenance and exchangeability,
- b) aluminium first wall components which last the full lifetime of the experiment, but have to be operated below 200 °C and are probably not suited for hot blanket experiments,
- c) passive liners which protect blanket cooling panels from surface erosion, serving as sacrificial elements which can be replaced easily.

Except of these conditions given by thermal stress and surface erosion, the first wall design will also be affected by other considerations like vacuum compatibility, plasma contaminations, radiation damage and others.

For the case that future large tokamak devices can be operated with material limiters rather than magnetic divertors, the present estimates show, that high thermal power levels of the order of 100 MW can be removed via toroidal limiters. If the limiter shape is chosen appropriately, the local power flux density can be kept at a tolerable level. For the concept of radiation cooled graphite limiters fairly high evaporation rates at elevated graphite temperatures have to be taken into account. If the maximum local power deposition on the limiter is below 3 MW/m^2 , the total evaporation rate is low enough to keep the carbon content in the plasma below 1 %. For calculations more accurate than the estimates presented here the understanding of plasma-limiter interaction must be improved by conducting appropriate experiments. After the limiter action and corresponding heat fluxes are evident, a more accurate analysis including two-dimensional heat conduction codes can be carried out.

Probably the most severe thermal loads experienced by the first wall show up during major plasma disruptions. The simple model used here to calculate the heat deposition during a disruption yields a maximum local erosion of about $50 \text{ }\mu\text{m}$ on graphite. This is too high for the first wall to stand 10^3 disruptions during its lifetime. The erosion could be even higher if the heat deposition is not uniform in the toroidal direction. On the other hand, it seems unlikely that the total energy will be dumped directly into the wall rather than being radiated onto larger wall areas by the impurities generated during the initial phase of the disruption. Experiments will have to be set up to explore the disruption time, the energy deposition profile and possibly the "virtual limiter" concept.

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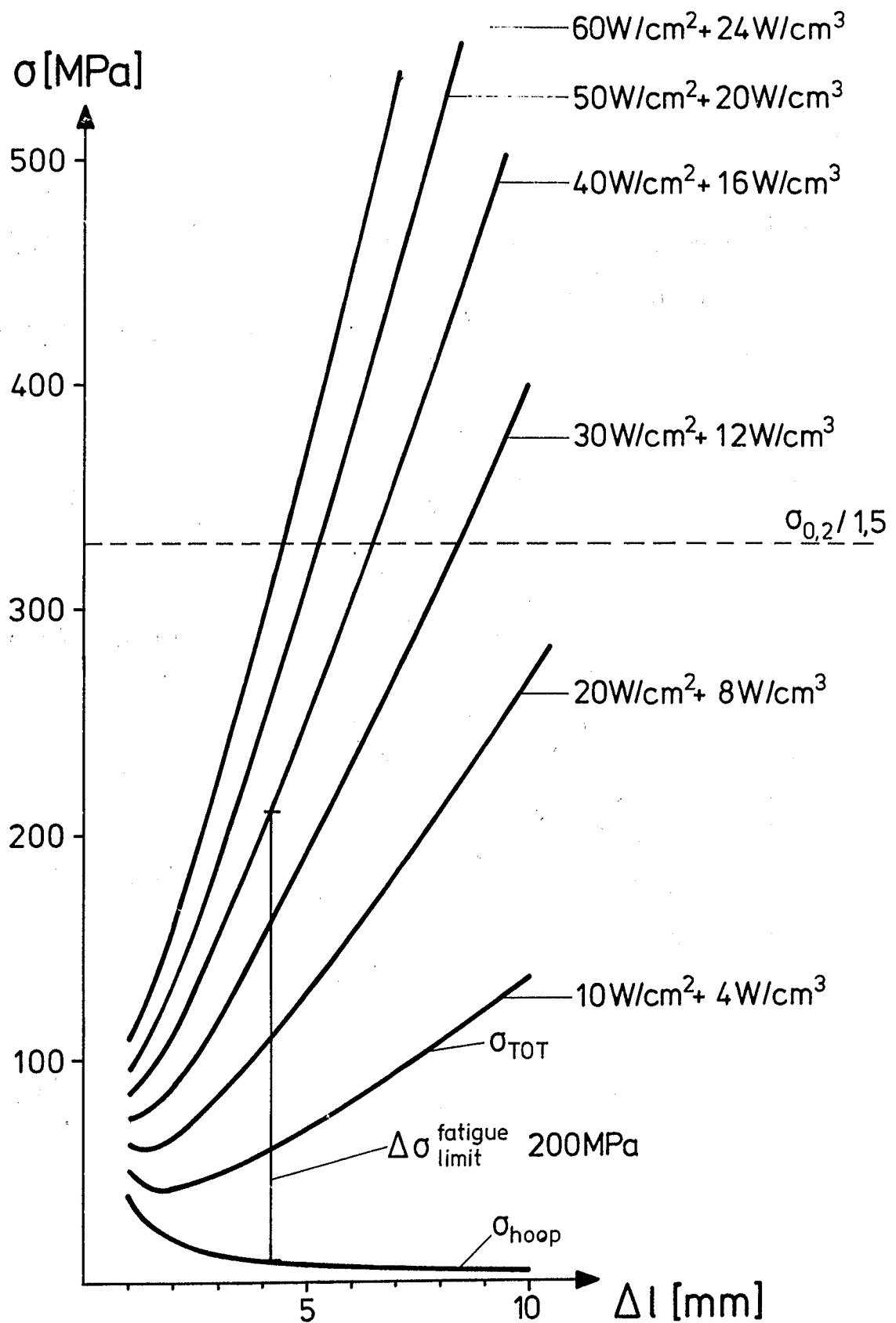


Fig.1

Stresses in a stainless steel tube of 4cm inner diameter for various heat fluxes as a function of wall thickness

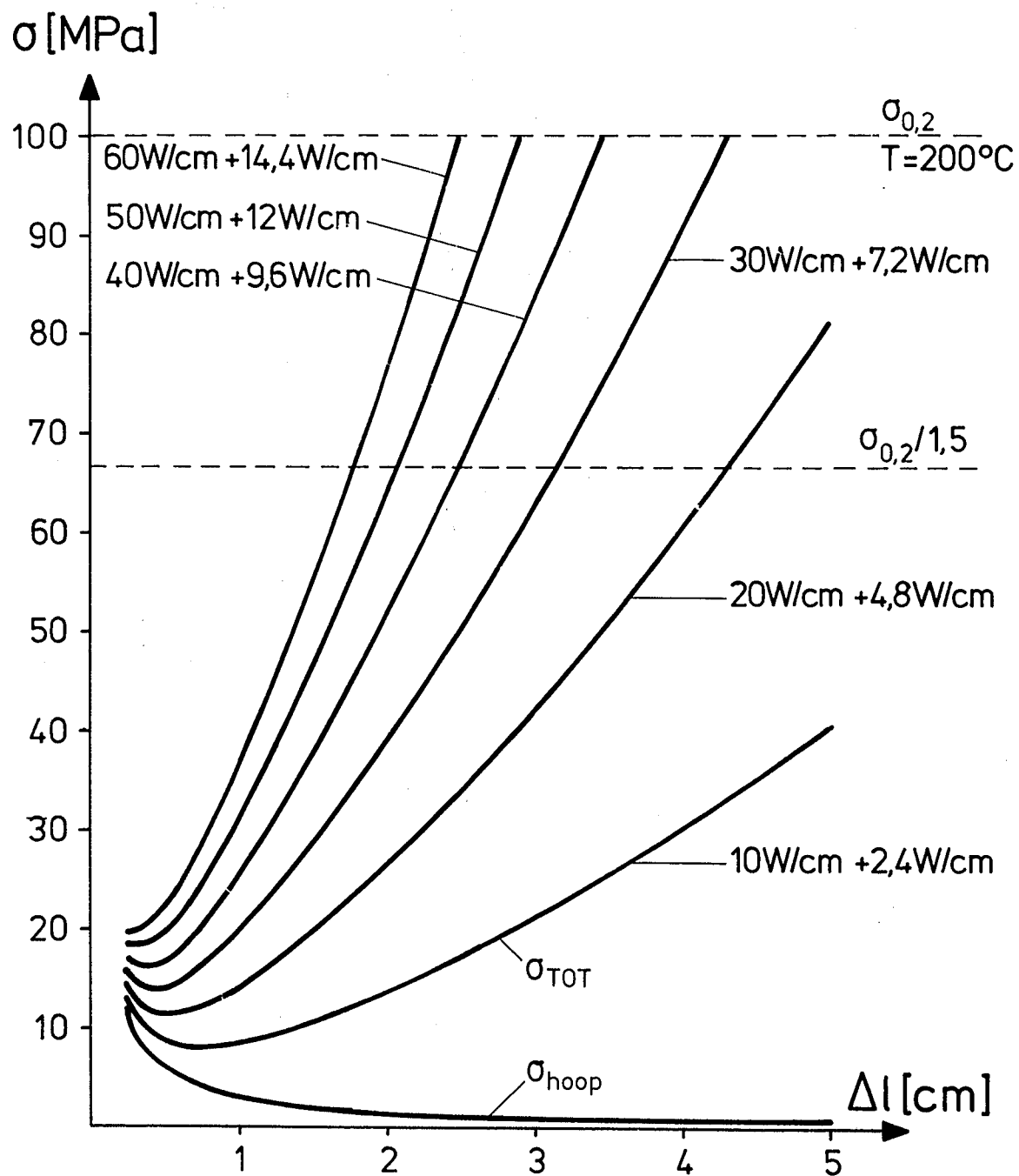


Fig. 2

Stresses in an aluminum tube of 4cm inner diameter for various heat fluxes as a function of wall thickness

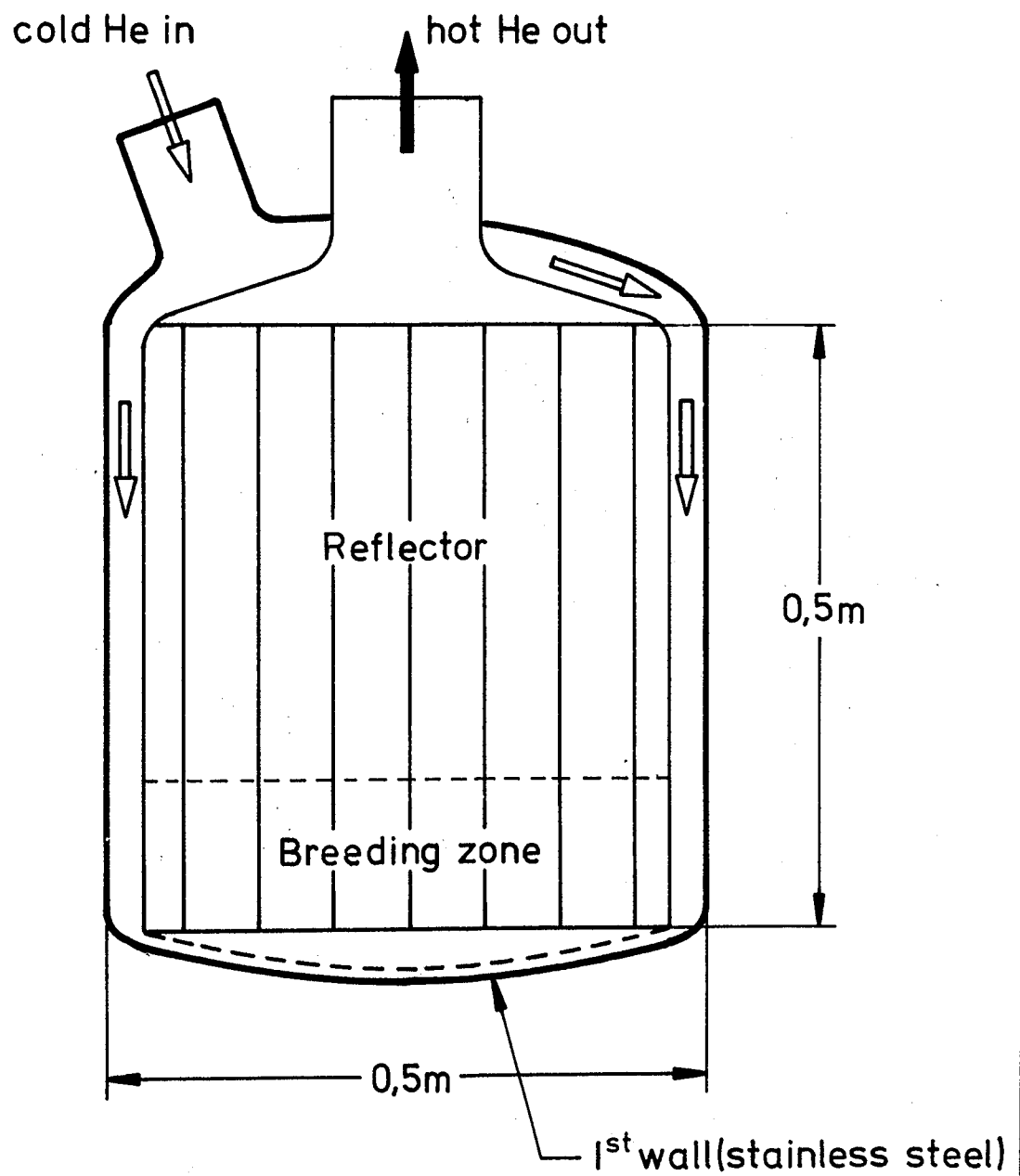


Fig. 3

Schematic of
blanket cell

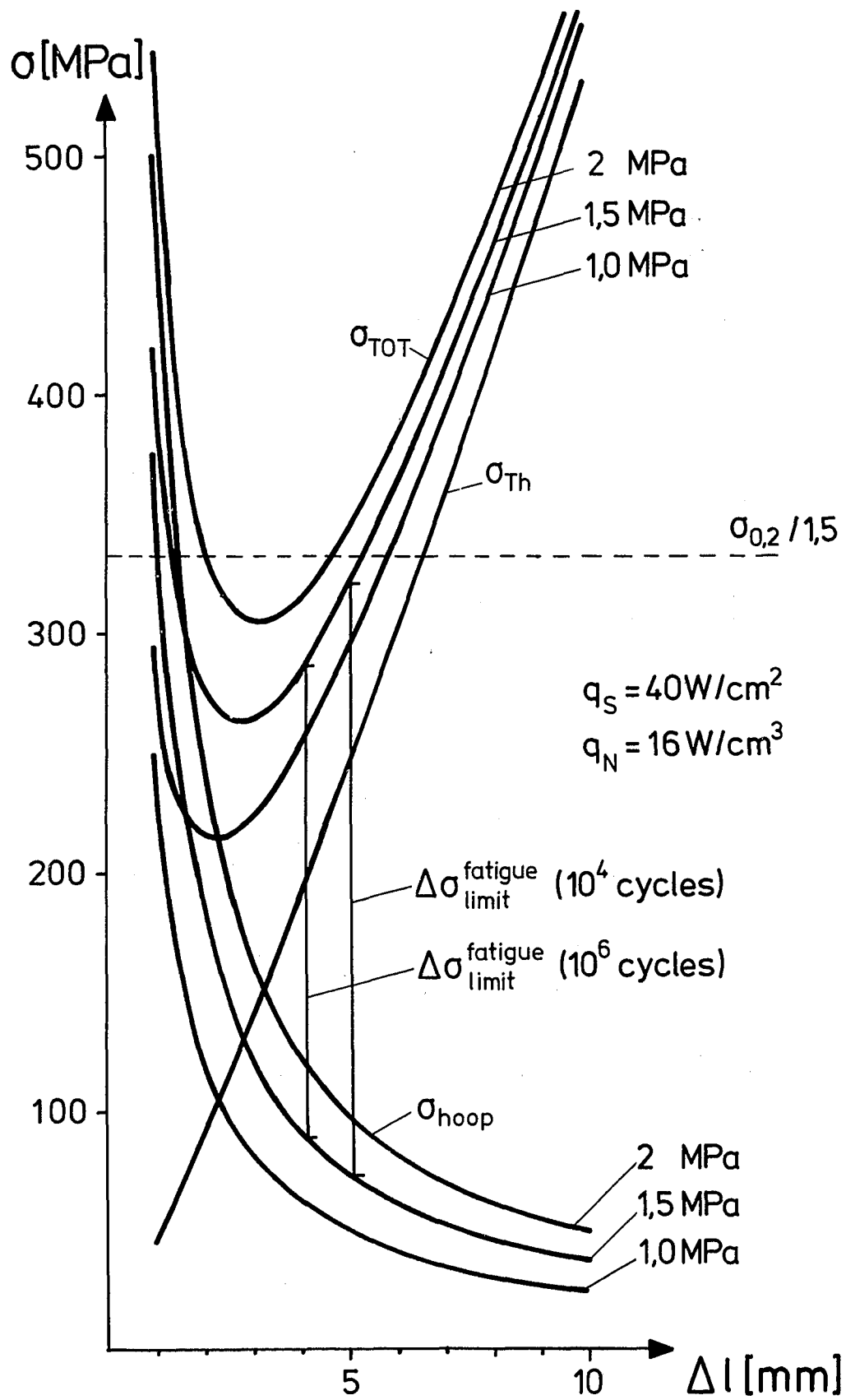


Fig. 4

Stresses in a blanket cell first wall exposed to various heat fluxes from the plasma as a function of wall thickness

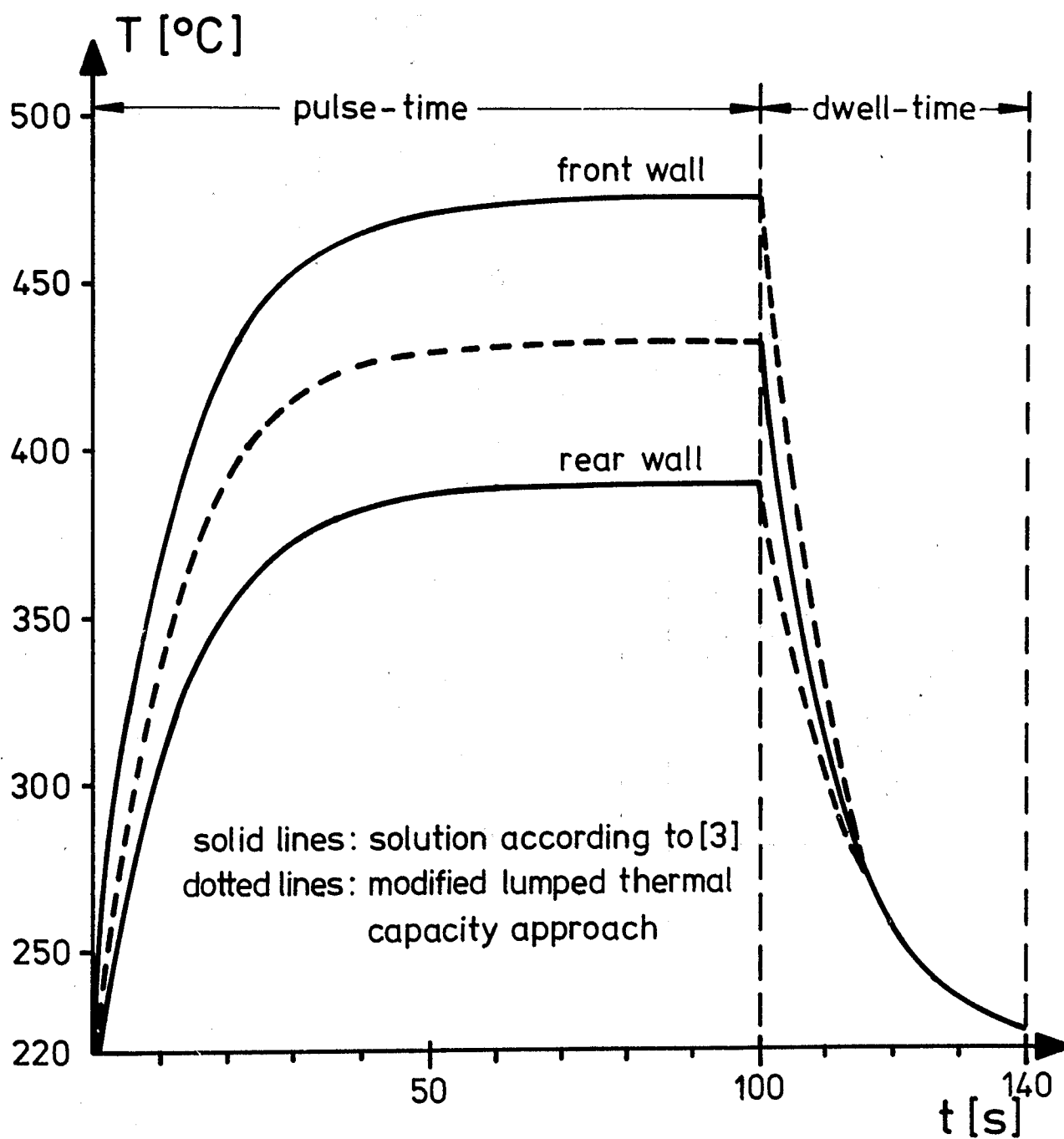
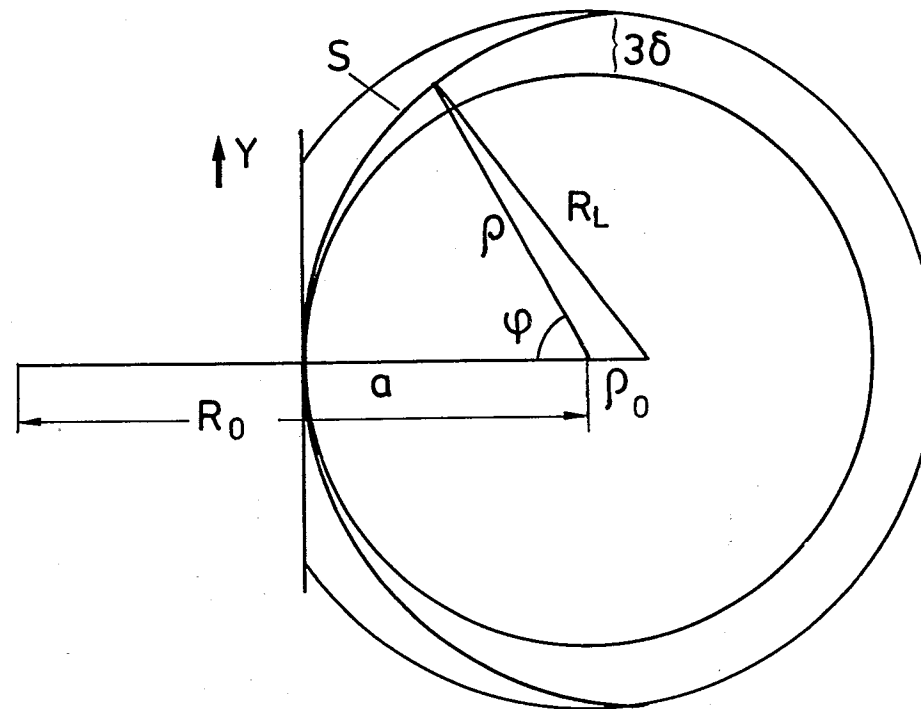


Fig.5

Temperature cycle of the
blanket cell first wall



R_L radius of curved
 S perimeter of limiter
 Y poloidal length of straight limiter
 R_0 major plasma radius
 a minor plasma radius
 δ e-folding length of scrape-off layer

Fig. 6

Geometry of plasma column and limiter configurations

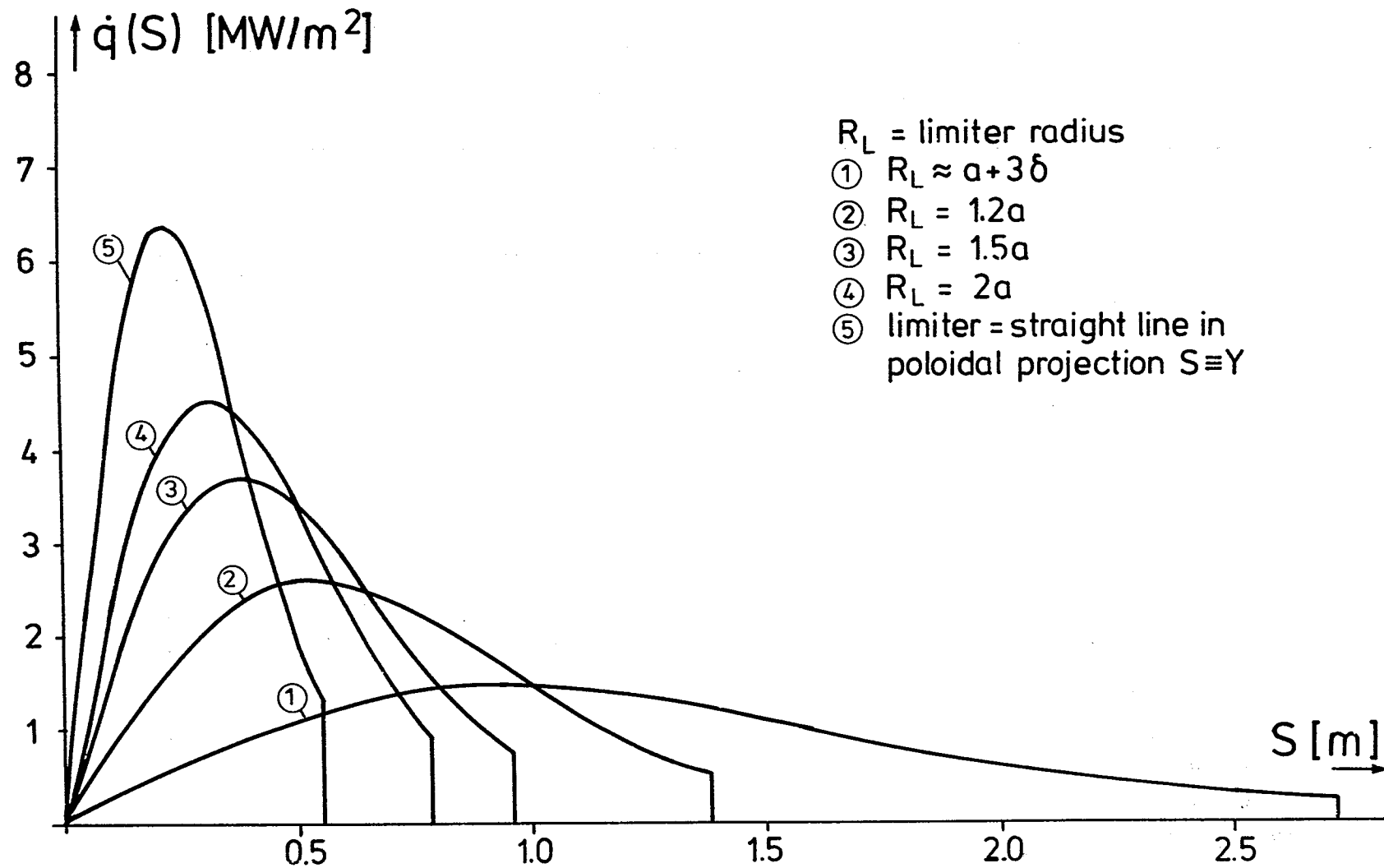


Fig.7

Heat load to various limiter configurations for circular plasma cross-section

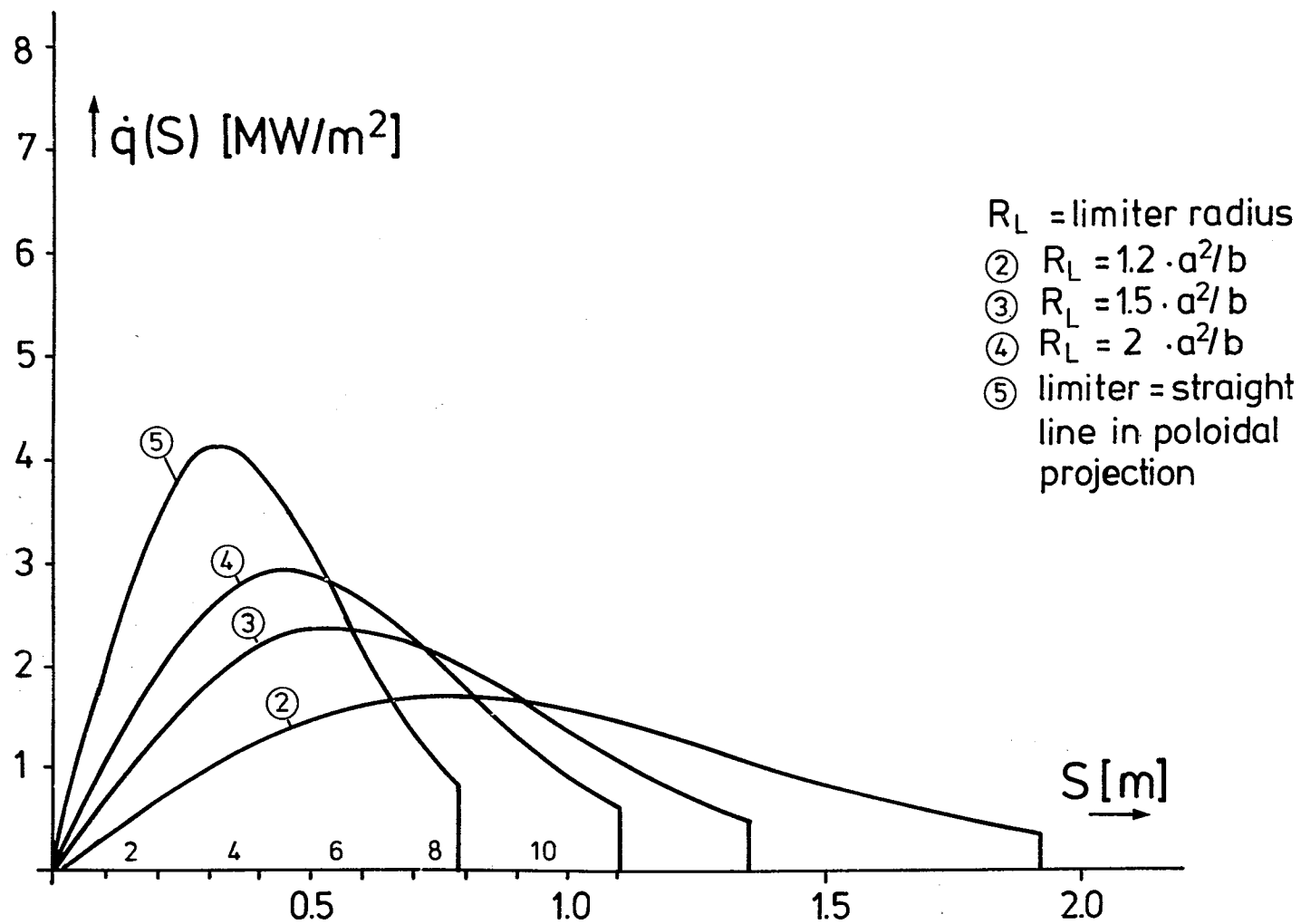


Fig. 8

Heat load to various limiter configurations for elliptical plasma cross-section

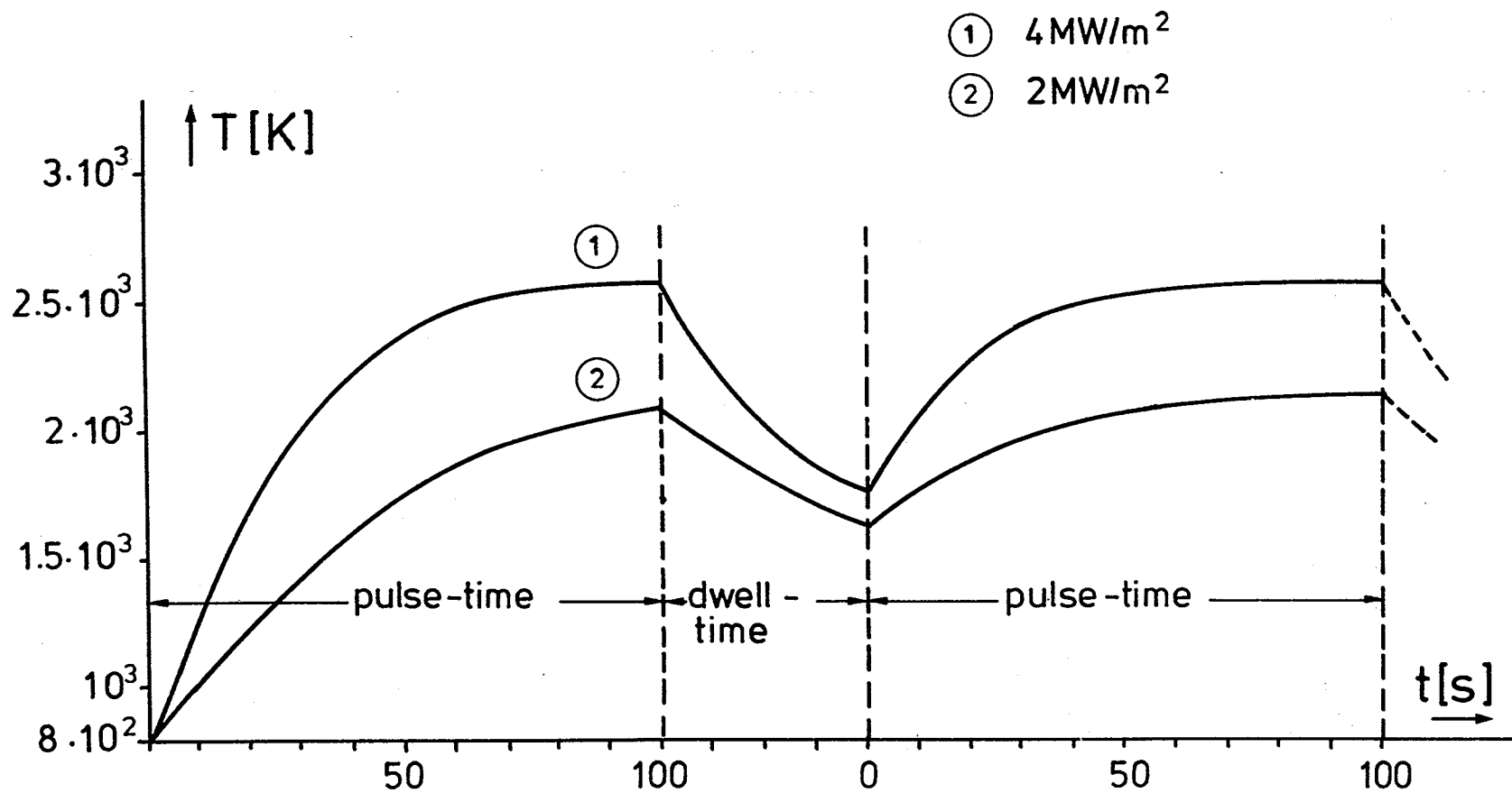


Fig.9

Heating and cooling of isolated graphite tile

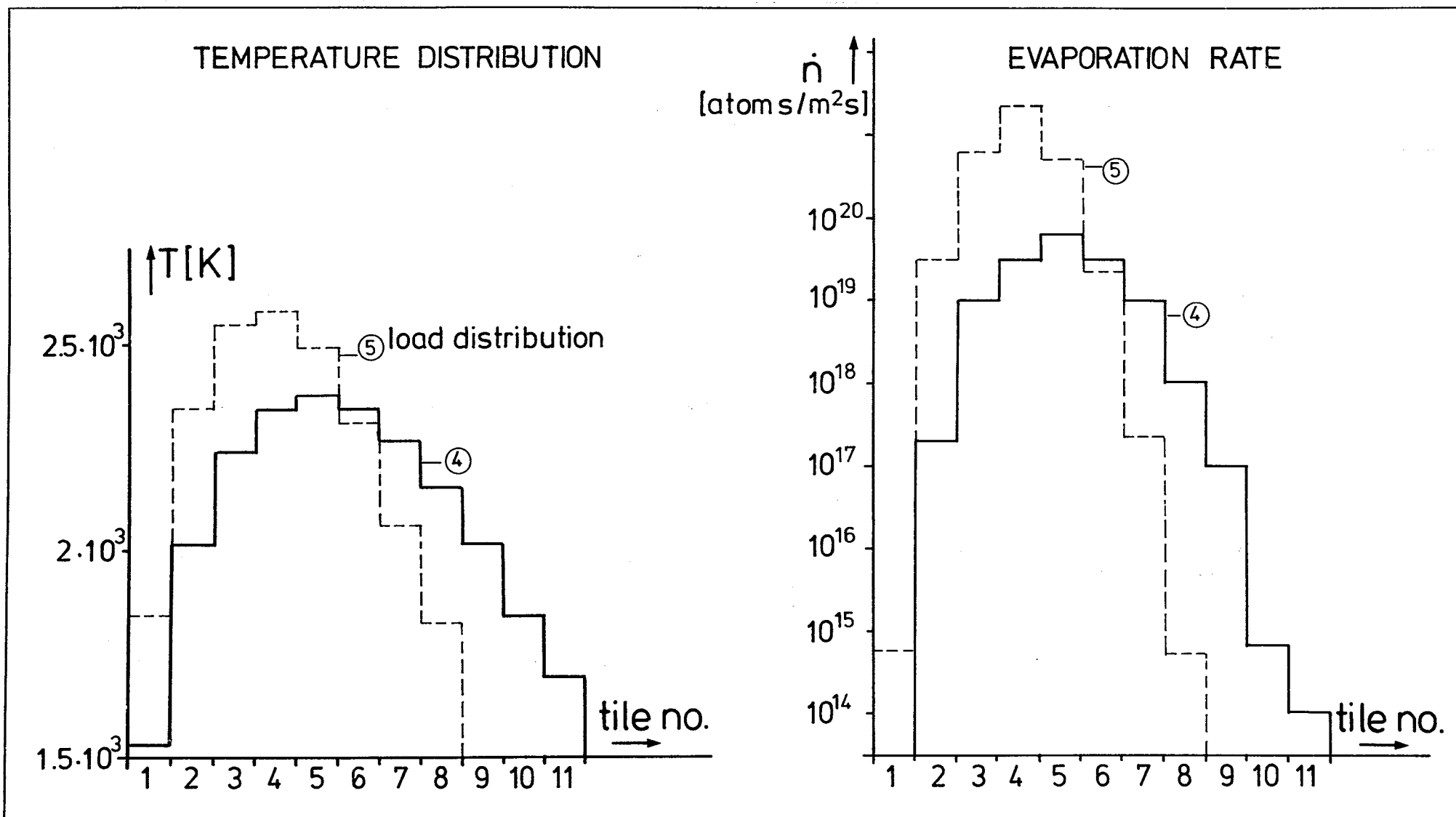


Fig.10

Temperature distribution and evaporation rates
on graphite tiles

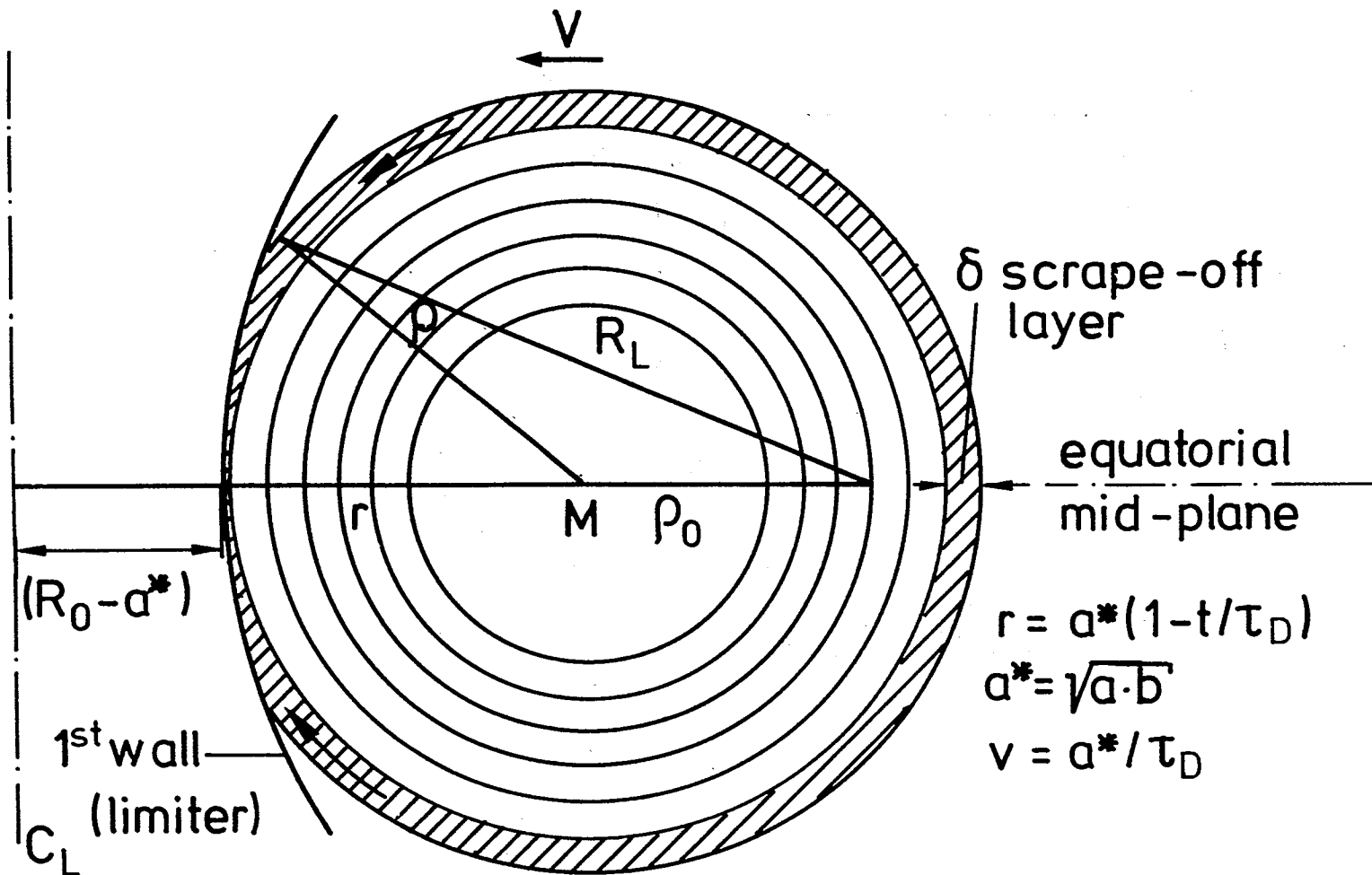
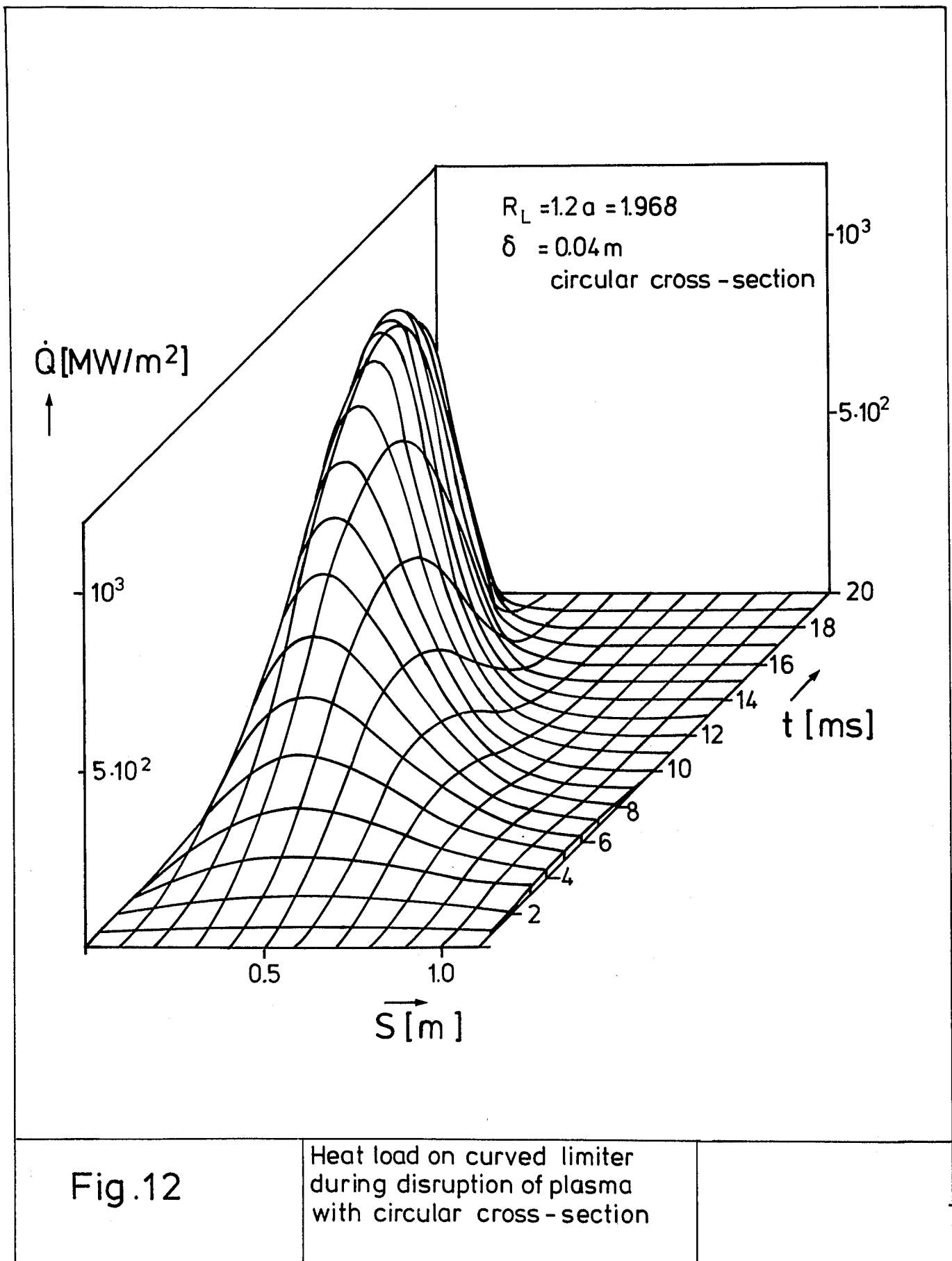


Fig.11

Poloidal projection of plasma column and
1st wall



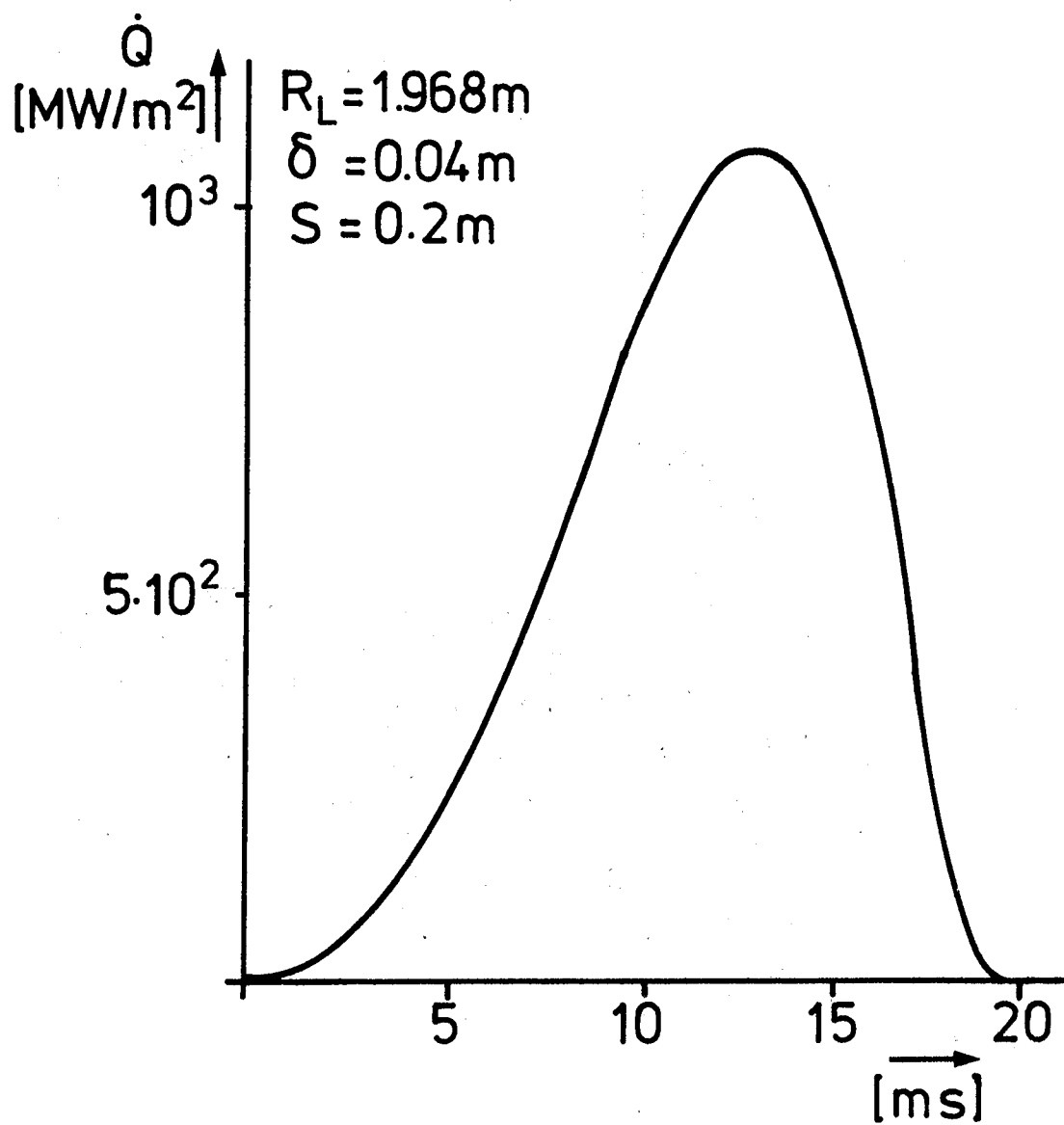


Fig. 13

Energy flux density as
function of time at
 $S=0.2\text{m}$ for circular
plasma cross-section

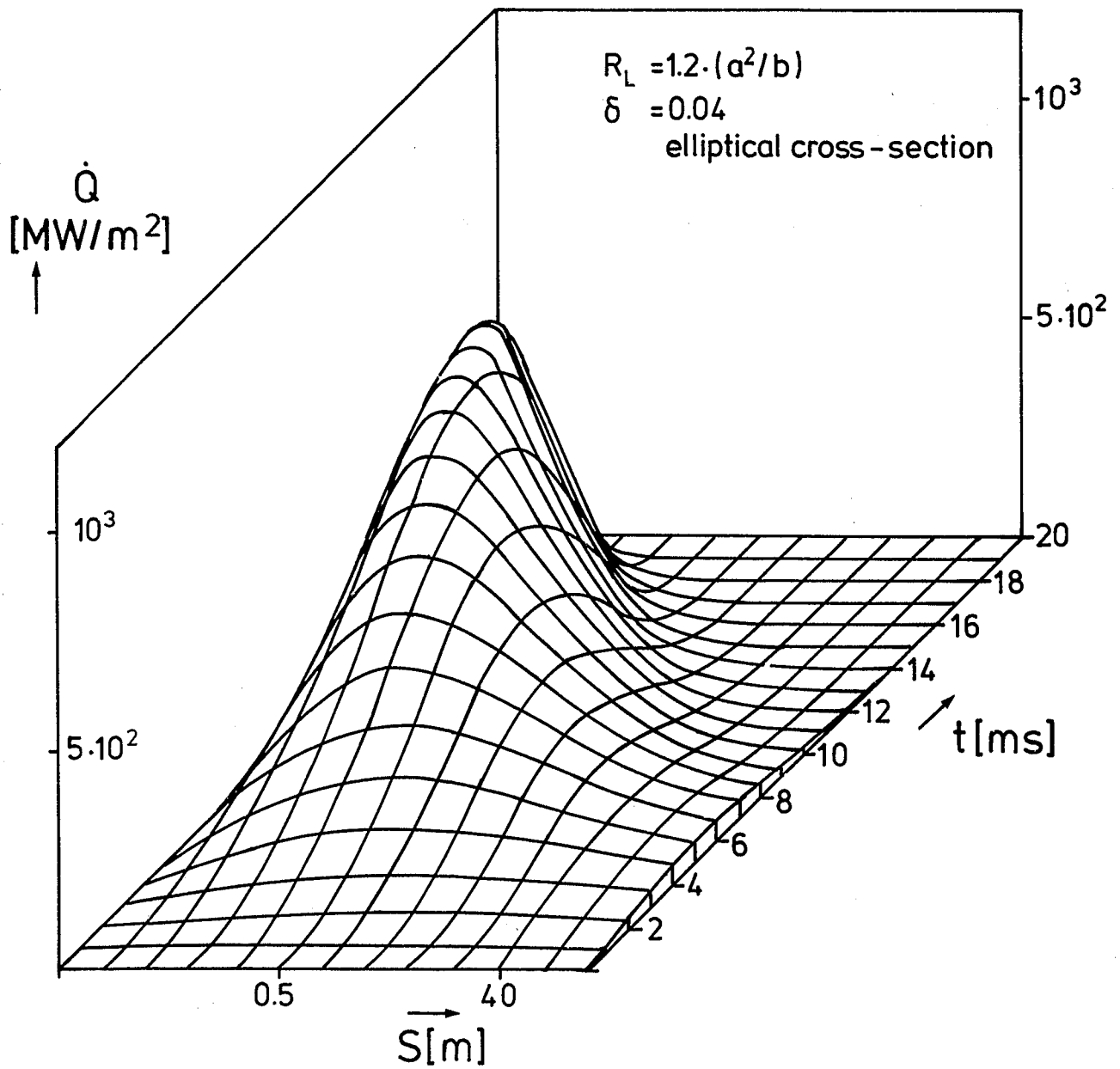


Fig.14

Heat load on curved limiter
during disruption of plasma
with elliptical cross-section

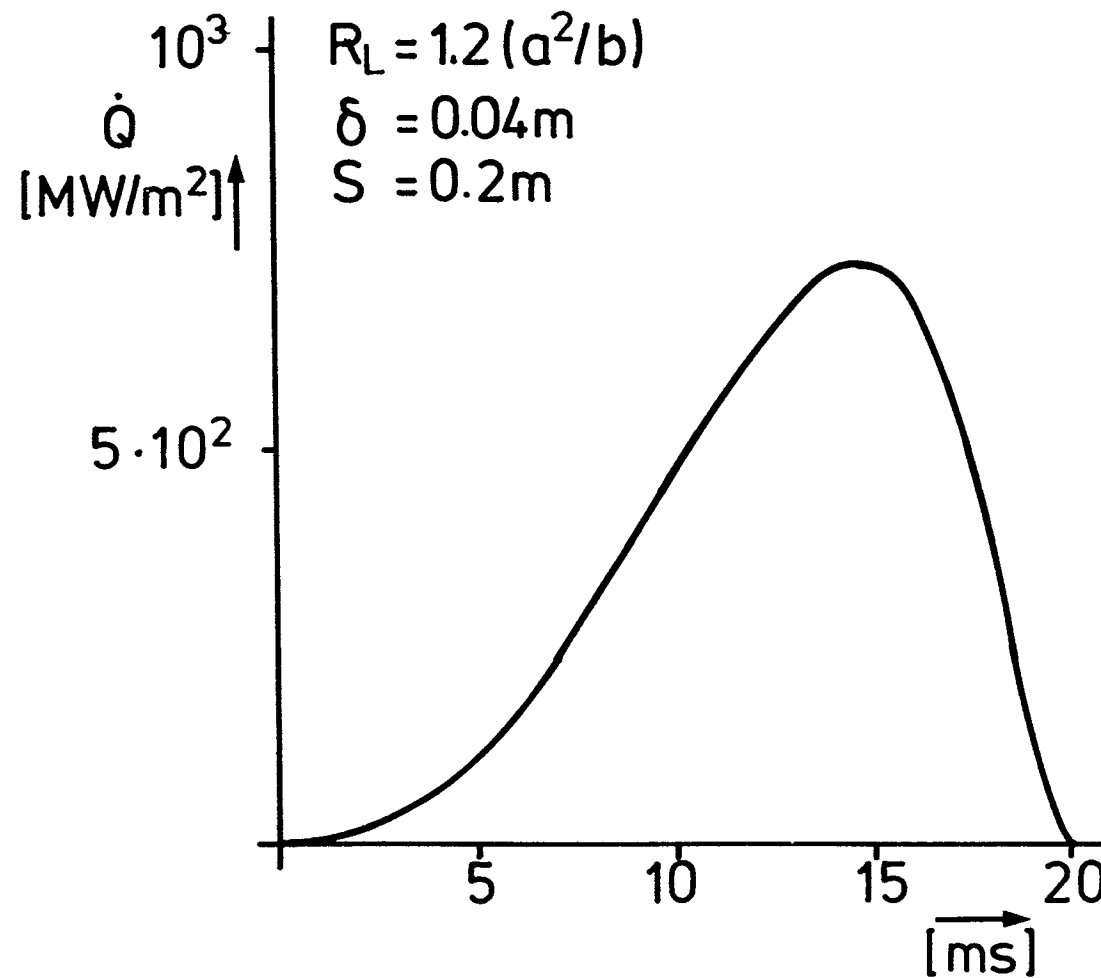


Fig.15

Energy flux density as function of time at $S=0.2\text{m}$ for elliptical plasma cross-section